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Chapter 28

SINGLE-PHASE IM TESTING

28.1 INTRODUCTION

The elliptic magnetic field in the airgap of single phase IMs in presence of space m.m.f. harmonics, magnetic saturation, rotor skin effect, and interbar rotor currents makes a complete theoretical modelling a formidable task.

In previous chapters, we did touch all these subjects through basically refined analytical approaches. Ideally, a 3D-FEM, with eddy currents computation and circuit model coupling should be used to tackle simultaneously all the above phenomena.

However, such a task still requires a prohibitive amount of programming and computation effort.

As engineering implies intelligent compromises between results and costs, especially for single-phase IMs, characterized by low powers, experimental investigation is highly recommended. But, again, it is our tendency to make tests under particular operation modes such as locked rotor (shortcircuit) and no-load tests to segregate different kinds of losses and then use them to calculate on-load performance.

Finally, on-load tests are used to check the loss segregation approach.

For three phase IMs, losses from segregation methods and direct on-load tests are averaged to produce safe practical values of stray load losses and efficiency (IEEE Standard 112B).

The presence of rotor currents even at zero slip ($S = 0$)-due to the backward field component-in single phase IM makes the segregation of losses and equivalent circuit parameter computation rather difficult. Among many potential tests to determine single phase IM parameters and loss segregation two of them have gained rather large acceptance.

One is based on single phase supplying of either main or auxiliary winding of the single phase IM at zero speed ($S = 1$) and on no-load. The motor may be started as capacitor or split-phase motor and then the auxiliary phase is turned off with the motor free at shaft. [1]

The second method is based on the principle of supplying the single phase IM from a symmetrical voltage supply. The auxiliary winding voltage V_a is 90° ahead of the main winding voltage and $V_a = V_m a$, such that the current I_a is I_m/a ; a is the ratio between main and auxiliary winding effective turns. [2]

This means in fact that pure forward travelling field conditions are provided. The 90° shifted voltage source is obtained with two transformers with modified Scott connection and a Variac.

Again shortcircuit (zero speed)-at low voltage-and no load testing is excersized. Moreover, ideal no-load operation ($S = 0$) is performed by using a drive at synchronous speed $n_1 = f_1/p_1$

Then both segregation methods are compared with full (direct) load testing. [2] For the case studies considered both methods claim superior results. [1,2]

The two methods have a few common attributes

- They ignore the stray losses (or take them as additional core losses already present under no load tests).
- They consider the magnetization inductance as constant from shortcircuit ($S = 1$) to no-load and load conditions.
- They ignore the space m.m.f. harmonics, and, in general, consider the current in the machine as sinusoidal in time.
- They neglect the skin effect in the rotor cage. Though the value of the rotor slot depth is not likely to go over $15 \times 10^{-3} \text{m}$ (the power per unit is limited to a few kW), the backward field produces a rotor current component whose frequency $f_{2b} = f_1(2-S)$ varies from f_1 to $2f_1$ when the motor accelerates from zero to rated speed. The penetration depth of electromagnetic field in aluminum is about $12 \times 10^{-3} \text{m}$ at 50Hz and $(12/\sqrt{2}) \times 10^{-3} \text{m}$ at 100Hz. The symmetrical voltage method [2] does not have to deal with the backward field and thus the rotor current has a single frequency ($f_{2r} = Sf_1$). Consequently the skin effect may be neglected. The trouble is that during variable load operation, the backward field exists and thus the rotor skin effect is present. In the single phase voltage method [1] the backward field is present under no load and thus it may be claimed that somehow the skin effect is accounted for. It is true that, as for both methods the shortcircuit tests ($S = 1$) are made at rated frequency, the rotor resistance thus determined already contains a substantial skin effect. This may explain why both methods give results which are not far away from full load tests.
- Avoiding full load tests, both methods measure under no-load tests, smaller interbar currents losses in the rotor than under load. Though there are methods to reduce the interbar currents, there are cases when they are reported to be important, especially with skewed rotors. However, in this case, the rotor surface core additional losses are reduced and thus some compensation of errors may occur to yield good overall loss values.
- Due to the applied simplifications, neither of the methods is to be used to calculate the torque/speed curve of the single-phase IM beyond the rated slip ($S > S_n$). Especially for tapped winding or split-phase IMs which tend to have a marked third order m.m.f. space harmonic that causes a visible deep in the torque/speed curve around 33% of ideal no load speed ($S = 0$).

As the symmetrical voltage method of loss segregation and parameter computation is quite similar to that used for three phase IM testing (Chapter 22),

we will concentrate here on Veinott's method [1] as it sheds more light on single phase IM peculiarities, given by the presence of backward travelling field. The presentation here will try to retain the essentials of Veinott's method while making it show a simple form, for the potential user.

28.2 LOSS SEGREGATION THE SPLIT PHASE AND CAPACITOR START IMs

The split phase and capacitor start IMs start with the auxiliary winding on but end up operating only with the main stator winding connected to the power grid.

It seems practical to start with this case by exploring the shortcircuit (zero speed) and no load operation modes with the main winding only on, for parameter computation and loss segregation.

We will make use of the cross field model (see Chapter 24, [Figure 24.21](#)), though the travelling field (+, -) model would give similar results as constant motor parameters are considered.

For the zero speed test ($S = 1$) $Z_f = Z_b$ and, thus,

$$R_{sm} + R_{rm} \approx \frac{P_{sc}}{I_{msc}^2} \quad (28.1)$$

$$|Z_{sc}| = \frac{V_s}{I_{msc}} \quad (28.2)$$

$$X_{sm} + X_{rm} \approx \sqrt{\left(\frac{V_s}{I_{msc}}\right)^2 - (R_{sm} + R_{rm})^2} \quad (28.3)$$

With R_{sm} d.c. measured and temperature-corrected, and $X_{sm} \approx X_{rm}$ for first iteration the values of R_{sm} , $X_{sm} = X_{rm}$ and R_{rm} are determined. For the no-load test (still $I_a = 0$) we may measure the slip value S_0 or we may not. If we do, we make use of it. If not, $S_0 \approx 0$.

Making use of the equivalent circuit of [Figure 28.1](#), for $S = S_0 = 0$ and with $I_{m0}(A)$, $V_s(V)$, $P_m(W)$ and E_a measured, we have the following mathematical relations

$$Z_f \approx \frac{1}{2} jX_{mm} \quad (28.4)$$

$$Z_b \approx \frac{1}{2} \left[\frac{R_{rm}}{2} + jX_{rm} \right] \quad (28.5)$$

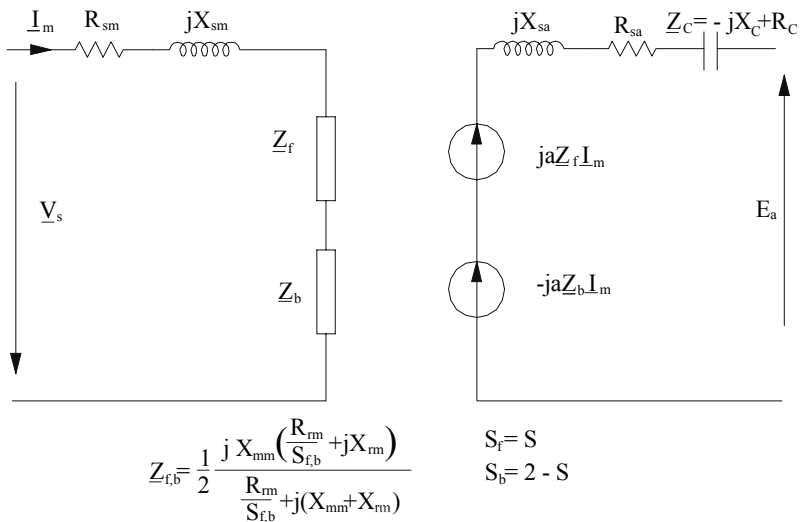


Figure 28.1 The cross field single phase IM with auxiliary winding open ($I_a = 0$)

From

$$\frac{E_a}{a} \approx (Z_f - Z_b) I_{m0} \quad (28.6)$$

with R_{rm} and X_{sm} already determined from the shortcircuit test and I_{m0} and E_a measured, we need the value of a in Equation 28.6 to determine the magnetization reactance X_{mmm}

$$X_{mmm} = 2 \sqrt{\frac{E_a^2}{a^2 I_{m0}^2} - \frac{R_{rm}^2}{16}} - X_{rm} \quad (28.7)$$

A rather good value of a may be determined by running on no load the machine additionally, with the main winding open and the auxiliary winding fed from the voltage $V_{a0} \approx 1.2E_a$. With E_m measured, [1]

$$a = \sqrt{\frac{E_a V_{a0}}{V_s E_m}} \quad (28.8)$$

Once X_{mmm} is known, from (28.7) with (28.8), we may make use of the measured P_{m0} , I_{m0} , V_s (see Figure 28.1) to determine the sum of iron and mechanical losses

$$p_{mec} + p_{iron} = P_{m0} - \left(R_{sm} + \frac{R_{rm}}{4} \right) I_{m0}^2 \quad (28.9)$$

The no load test may be performed at different values of V_s , below rated value, until the current I_{m0} starts increasing; a sign that the slip is likely to increase too much.

As for the three phase IM, the separation of mechanical and core losses may be done by taking the ordinate at zero speed of the rather straight line dependence of $(p_{mec} + p_{iron})$ of V_s^2 (Figure 28.2). Alternatively, we may use only the results for two voltages to segregate p_{mec} from p_{iron} .

A standard straight-line curve fitting method may be used for better precision.

The core loss is, in fact, dependent on the e.m.f. E_m (not on input voltage V_m) and on the magnetic field ellipticity.

The field ellipticity decreases with load and this is why, in general, the core losses are attributed to the forward component.

Consequently, the core loss resistance R_{miron} may be placed in series with the magnetization reactance X_{mm} and thus (Figure 28.3)

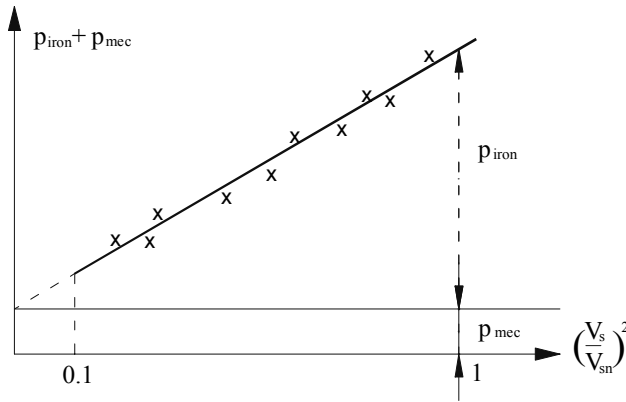


Figure 28.2 Mechanical plus core losses at no load (open auxiliary winding)

$$R_{miron} \approx \frac{p_{iron}}{2I_{m0}^2} \quad (28.10)$$

The impedance at no load Z_{om} is

$$|Z_{om}| = \frac{V_s}{I_{m0}} = \sqrt{\left(R_{sm} + R_{miron} + \frac{R_{rm}}{4}\right)^2 + \left(\frac{X_{mm} + X_{rm}}{2} + X_{sm}\right)^2} \quad (28.11)$$

Equation (28.11) allows us to calculate again X_{mm} . An average of the value obtained from (28.7) and (28.11) may be used for more confidence.

Now that all parameters are known, it is possible to refine the results by introducing the magnetization reactance X_{mm} in the shortcircuit impedance, while still $X_{sm} = X_{rm}$, to improve the values of R_{rm} and X_{sm} until sufficient convergence is obtained.

Today numerical methods available through many software programs on PCs allow for such iterative procedures to be applied rather easily.

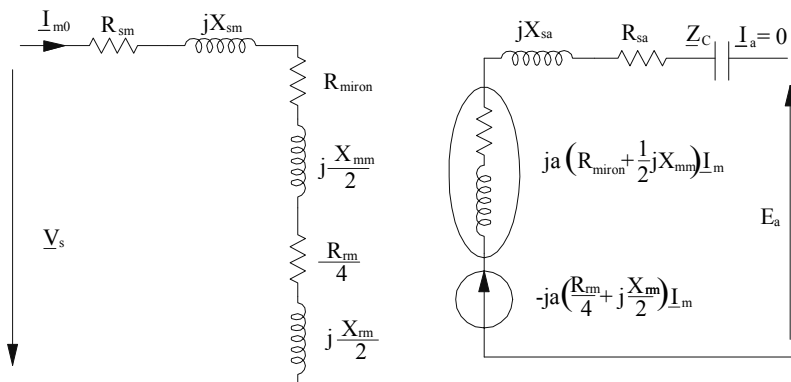


Figure 28.3 Simplified no load ($S = 0$) equivalent circuit with series core loss resistance R_{miron} in both circuits (the auxiliary winding is open)

Example 28.1

A 123 W (1/6 hp), 6 poles, 60 Hz, 110 V, split phase motor was tested as follows [1]

$R_{sm} = 2.54 \Omega$ after locked rotor reading, $R_{sm} = 2.65 \Omega$ after no-load single phase running.

Locked rotor watts at 110 V is $P_{sc} = 851$ W.

No-load current I_{m0} at $V_{s0} = 105$ V is $I_{m0} = 2.68$ A. Locked rotor current is $I_{sc} = 11.65$ A.

$$V_{s0} = V_{sn} - I_{mn} R_{sm}^h \cos \phi_n = 110 - 3.17 \times 2.85 \times 0.588 \approx 105 \text{ V} \quad (28.12)$$

where $I_{mn} = 3.17$ A, $R_{sm}^h = 2.85 \Omega$, $\cos \phi_n = 0.588$, full load slip $S_n = 0.033$. Full load input $P_{1n} = 205$ W and rated efficiency $\eta_n = 60.5\%$ have been obtained from a direct load (brake) test.

The no-load power versus applied volts V_s , produces $(p_{iron})_{V_{s0}} = 24.7$ W and mechanical losses $p_{mec} = 1.5$ W.

The no-load auxiliary winding voltage $E_a = 140.0$ V.

The value of turns ratio a is found from an additional no-load test with the auxiliary winding fed at $1.2 E_a$ (V). With the measured no-load main winding voltage $E_{m0} = 105$ V, a is (28.8)

$$a = \sqrt{\frac{140 \times 140 \times 1.2}{105 \times 105}} = 1.46$$

Let us now find the motor parameters and directly check the efficiency measured by the loss segregation method.

The rotor resistance (referred to the main winding) R_{rm} is (28.1)

$$R_{rm} = \frac{851}{11.65^2} - 2.54 = 3.73\Omega$$

The stator and rotor leakage reactances $X_{rm} = X_{sm}$ from (28.3) are

$$X_{sm} = X_{rm} = \frac{1}{2} \sqrt{\left(\frac{105}{11.65}\right)^2 - (3.73 + 2.54)^2} = 3.237\Omega$$

R_{rm} is getting larger during the no load test, due to heating, to the same extent that R_{sm} does

$$R_{rm}^o = R_{rm} \times \frac{R_{sm}^o}{R_{sm}} = 3.73 \times \frac{2.65}{2.54} = 3.89\Omega$$

Now the magnetisation reactance X_{mm} , from (28.7), is

$$X_{mm} = 2 \sqrt{\left(\frac{140.0}{1.46 \times 2.58}\right)^2 - \left(\frac{3.89}{4}\right)^2} - 3.237 = 77.57\Omega$$

with the core resistance R_{miron} (28.10)

$$R_{miron} = \frac{p_{iron}}{2I_{m0}^2} = \frac{24.7}{2 \cdot 2.58^2} = 1.87\Omega$$

Now from (28.11) we may recalculate X_{mm}

$$X_{mm} = 2 \sqrt{\left(\frac{105}{2.58}\right)^2 - \left(2.65 + 1.87 + \frac{3.89}{4}\right)^2} - 3.237 = 74.71\Omega$$

An average of the two X_{mm} values would be 76.1235Ω .

By now the parameter problem has been solved. A few iterations may be used with the complete circuit at $S = 1$ to get better values for R_{rm} and $X_{sm} = X_{rm}$. However, we should note that $X_{mm} / X_{rm} \approx 20$, and thus not much is to be gained from these refinements.

For efficiency checking we need to calculate the winding losses at $S_n = 0.033$ with the following parameters

$$R_{sm}^h = 2.85\Omega, R_{rm}^h = R_{rm}^o \times \frac{R_{sm}^h}{R_{sm}^o} = 3.89 \times \frac{2.85}{2.65} = 4.1835\Omega$$

$$X_{sm} = X_{rm} = 3.237\Omega, X_{mm} = 76.12\Omega$$

The core resistance R_{miron} may be neglected when the rotor currents are calculated

$$I_{\text{rmf}} = I_m \left| \frac{j \cdot X_{\text{mm}}}{\frac{R_{\text{rm}}^h}{S_n} + j \cdot (X_{\text{mm}} + X_{\text{rm}})} \right| = 3.117 \cdot \left| \frac{j \cdot 76.12}{\frac{4.1835}{0.033} + j \cdot (76.12 + 3.237)} \right| = 1.586 \text{ A}$$

$$I_{\text{rmb}} \approx I_m = 3.117 \text{ A}$$

So the total rotor winding losses p_{Corotor} are

$$p_{\text{Corotor}} = I_{\text{rmf}}^2 \frac{R_{\text{rm}}^h}{2} + I_{\text{rmb}}^2 \frac{R_{\text{rm}}^h}{2} = (3.117^2 + 1.586^2) \frac{4.1835}{2} = 25.58 \text{ W}$$

The stator copper losses p_{cos} are

$$p_{\text{cos}} = I_{\text{m0}}^2 R_{\text{sm}}^h = 3.117^2 \times 2.85 = 27.69 \text{ W}$$

The total load losses Σp from loss segregation, are

$$\Sigma p = p_{\text{cos}} + p_{\text{Corotor}} + p_{\text{iron}} + p_{\text{mec}} = 27.69 + 25.58 + 24.7 + 1.5 = 79.47 \text{ W}$$

The losses calculated from the direct load test are

$$(\Sigma p)_{\text{load}} = P_{\text{in}} - P_{\text{out}} = 205 - 123 = 82 \text{ W}$$

There is a small difference of 2 W between the two tests, which tends to validate the methods of loss segregation. However, it is not sure that this is the case for most designs.

It is recommended to back up loss segregation by direct shaft loading tests whenever possible.

It is possible to define the stray load losses as proportional to stator current squared and then to use this expression to determine total losses at various load levels

$$\text{stray load losses} = (\Sigma p)_{\text{load}} - \Sigma p_{\text{segregation}} \approx R_{\text{stray}} [I_m^2 + (I_a a)^2]$$

R_{stray} may then be lumped into the stator resistances R_{sm} and R_{sa} / a^2 .

28.3 THE CASE OF CLOSED ROTOR SLOTS

In some single phase IMs (as well as three phase IMs) closed rotor slots are used to reduce noise.

In this case the rotor slot leakage inductance varies with rotor current due to the magnetic saturation of the iron bridges above the rotor slots.

The shortcircuit test has to be done now for quite a few values of voltage (Figure 28.4)

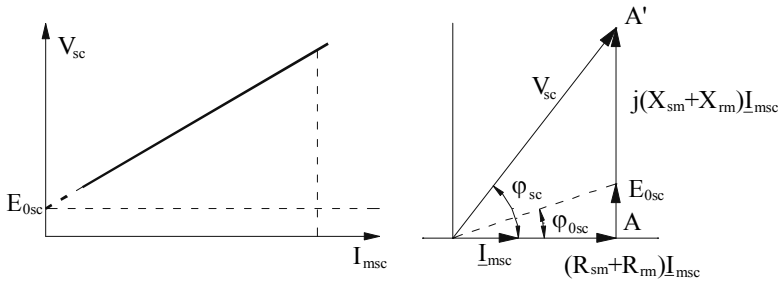


Figure 28.4 Shortcircuit characteristics

In this case, the equivalent circuit should additionally contain a constant e.m.f. E_{0sc} , corresponding to the saturated closed rotor upper iron bridge (Figure 28.5).

Only the segment $AA'-E_{0sc}$ (on Figure 28.5) represents the voltage drop on the rotor and stator constant leakage reactances.

$$X_{sm} + X_{rm} = \frac{V_{sc} \sin \varphi_{sc} - E_{0sc}}{I_{msc}} \quad (28.13)$$

28.4 LOSS SEGREGATION THE PERMANENT CAPACITOR IM

The loss segregation for the permanent capacitor IM may be performed as for the single phase IM, with only the main winding ($I_a = 0$) activated.

The auxiliary winding resistance and leakage reactance R_{sa} and X_{sa} may be measured, in the end, by a shortcircuit (zero speed) test performed on the auxiliary winding

$$P_{sca} \approx (R_{sa} + R_{ra}) I_{sca}^2 \quad (28.14)$$

$$X_{sa} + X_{ra} = \sqrt{\left(\frac{V_{sca}}{I_{sca}}\right)^2 - (R_{sa} + R_{ra})^2} \quad (28.15)$$

When $X_{ra} = a^2 X_{rm}$ (with a and X_{rm} known, from (28.13)) it is possible to calculate X_{sa} with measured input power and current. With R_{sa} d.c. measured, R_{ra} may be calculated from 28.14.

The capacitor losses may be considered through a series resistance R_C (see Figure 28.1). The value of R_C may be measured by separately supplying the capacitor from an a.c. source.

The active power in the capacitor P_C and the current through the capacitor I_C are directly measured

$$R_c = \frac{P_c}{I_c^2} \quad (28.16)$$

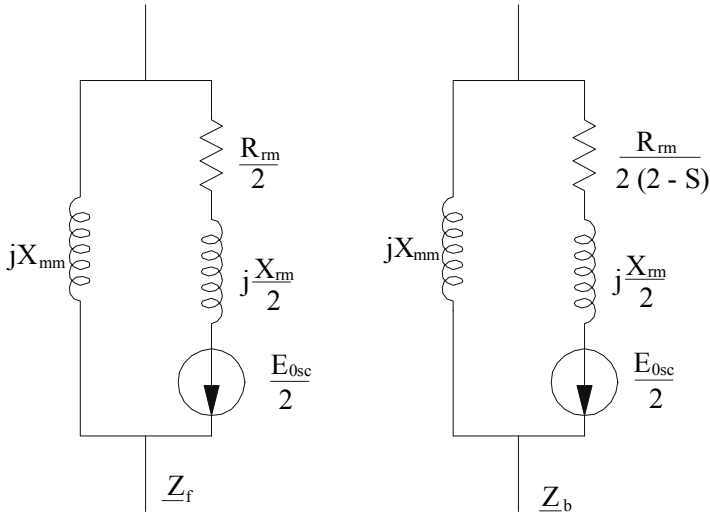


Figure 28.5 The forward (f, b) impedances for the closed slot rotor

Once all parameters are known, the equivalent circuit in [Figure 28.1](#) allows for the computation of both stator currents, I_m and I_a , under load with both phases on, and given slip value, S . Consequently, the stator and rotor winding losses may then be determined.

From this point on, we repeat the procedure in the previous paragraph to calculate the total losses by segregation method and from direct input and output measurements, with the machine shaft loaded.

28.5 SPEED (SLIP) MEASUREMENTS

One problem encountered in the load tests is the slip (speed) measurement.

Unless a precision optical speedometer (with an error in the range of 2 rpm or less) is available, it is more convenient to measure directly the slip frequency Sf_1 .

The “old” method of using a large diameter circular shortcircuited coil with a large number of turns-to track the axial rotor leakage flux by a current Hall probe (or shunt), may be used for the scope.

The coil is placed outside the motor frame at the motor end which does not hold the cooling fan. Coil axis is concentric with the shaft.

The acquired current signal contains two frequencies Sf_1 and $(2-S)f_1$. An off-line digital software (or a low pass hardware) filter may be applied to the current signal to extract the Sf_1 frequency component.

The slip computation error is expected to be equivalent to that of a 2 rpm precision speedometer or better.

28.6 LOAD TESTING

There are two main operation modes to test the single phase IM on load the motor mode and the generator mode.

Under the motor mode the electric input power, P_{1e} , and the output mechanical power, P_{2m} , are measured. P_{2m} is in fact calculated indirectly from the measured torque T_{shaft} and speed n

$$P_{2m} = T_{shaft} \cdot 2\pi n \quad (28.16)$$

The torque is measured by a torquemeter (Figure 28.6). Alternatively the load machine may have the losses previously segregated such that at any load level the single-phase IM mechanical power P_{2m} may be calculated

$$P_{2m} = P_{3e} - \sum p_{loadmachine} \quad (28.17)$$

The load machine may be a PM d.c. or a.c. generator with resistive load, a hysteresis or an eddy current d.c. brake.

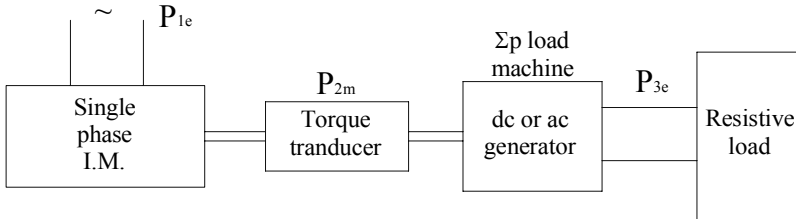


Figure 28.6 Load tests with a torquemeter

28.7 COMPLETE TORQUE-SPEED CURVE MEASUREMENTS

Due to magnetic saturation m.m.f. space harmonics, slotting, and skin effect influences on harmonic rotor currents, it seems that direct torque measurements at various speeds (below rated speed) is required.

Such a measurement may be performed by using a d.c. or an a.c. generator with power-converter energy retrieval to the power grid (Figure 28.7).

Alternatively a slow acceleration test on no load may be used to calculate the torque speed curve. For slow acceleration, the supply voltage may be lowered from V_{sn} to V_s .

The core losses are considered proportional to voltage squared. They are measured by the loss segregation method.

$$p_{\text{iron}} = (p_{\text{iron}})_{V_{\text{sn}}} \left(\frac{V_s}{V_{\text{sn}}} \right)^2 \quad (28.18)$$

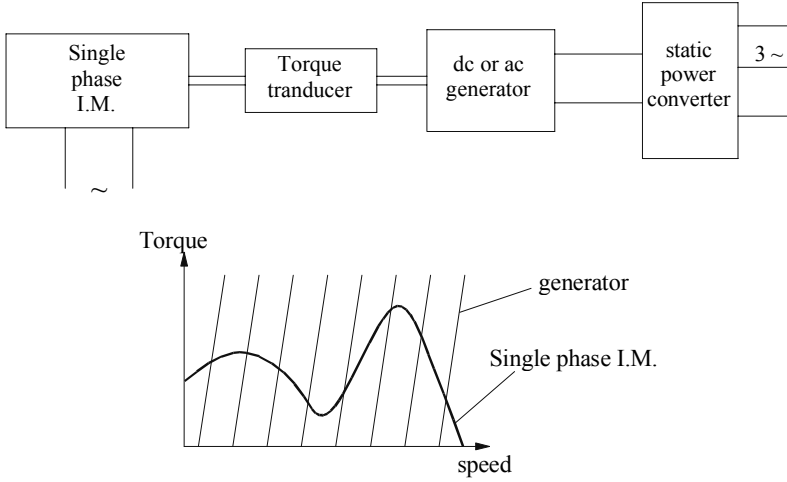


Figure 28.7 Torque/speed measurements with a torque meter

The mechanical losses proportional to speed squared

$$p_{\text{mec}} = (p_{\text{mec}})_{n_0} \left(\frac{n}{n_0} \right)^2 \quad (28.19)$$

With R_{sm} , R_{rm} , R_{sa} , X_{sm} , X_{ra} , X_{rm} , X_{mm} known and with I_m and I_a and input power measured P_1 , torque may be calculated as for steady state around rated speed.

$$T_e = \frac{p_{\text{mec}}}{2\pi n} + T_{\text{shaft}} = \frac{[P_1 - p_{\text{iron}} - R_{\text{sm}} I_m^2 - R_{\text{sa}} I_a^2 - R_{\text{rm}} (I_{\text{rmf}}^2 + I_{\text{rmb}}^2) - R_c I_a^2]}{2\pi n} \quad (28.20)$$

The computation of T_{shaft} is to be done offline. An optical speedometer could be used to measure the speed during the slow acceleration test and during a free deceleration after turn-off.

With $p_{\text{mec}}(n)$ known, the free deceleration test yields

$$J = - \frac{p_{\text{mec}}(n)}{2\pi \frac{dn}{dt}} \quad (28.21)$$

The moment of inertia J is thus obtained.

With J known and speed n acquired during the no-load slow acceleration test, the torque is

$$T_e(n) = J2\pi \frac{dn}{dt} + \frac{p_{mec}}{2\pi n} \quad (28.22)$$

The torque for rated voltage is considered to be

$$(T_e(n))_{V_{sn}} \approx (T_e(n))_{V_s} \left(\frac{V_{sn}}{V_s} \right)^2 \quad (28.23)$$

The speed derivative in (28.22) may be obtained offline, with an appropriate software filter, from the measured speed signal.

The two values of torque, (28.20) and (28.22), are then compared to calculate a measure of stray losses

$$p_{stray} \approx \left[(T_e)_{Equation(28.22)} - (T_e(n))_{Equation(28.20)} \right] \cdot \left(\frac{V_{sn}}{V_s} \right)^2 2\pi n \quad (28.24)$$

Temperature measurements methods are very similar to those used for three phase IMs. Standstill d.c. current decay tests may also be used to determine the magnetization curve $\Psi_{mm}(I_{mm})$ and even the resistances and leakage reactances, as done for three phase IMs.

28.8 SUMMARY

- The single phase IM testing aims to determine equivalent circuit parameters to segregate losses and to measure the performance on load; even to investigate transients.
- Due to the backward field (current) component, even at zero slip, the rotor current (loss) is not zero. This situation complicates the loss segregation in no load tests.
- The no load test may be done with the auxiliary phase open $I_a' = 0$, after the motor starts.
- The shortcircuit (zero speed) test is to be performed, separately for the main and auxiliary phases, to determine the resistances and leakage reactances.
- Based on these results the no load test (with $I_a = 0$) furnishes data for loss segregation and magnetization curve ($X_{mm}(I_{mm})$), provided it is performed for quite a few voltage levels below rated voltage.
- The calculation of rotor resistance R_{rm} from the zero speed test (at 50 (60) Hz) produces a value that is acceptable for computing on load performance, because it is measured at an average of forward and backward rotor current frequencies $\frac{Sf_1 + (2-S)f_1}{2} = f_1$.
- The difference between total losses by segregation method and by direct input / output measurements under load is a good measure of stray load losses. The stray load losses tend to be smaller than in three phase motors because the ratio between full load and no load current is smaller.

- Instead of single (main) phase no load and shortcircuit tests, symmetrical two voltage supplying for same tests has also been proved to produce good results.
- The complete torque-speed curve is of interest also. Below the breakdown torque speed value there may be a deep in the torque speed curve around 33 % of no load ideal speed (f_1 / p_1) due to the third space m.m.f. harmonic. A direct load method may be used to obtain the entire torque versus speed curve. Care must be exercised that the load machine had a rigid torque / speed characteristic to handle the statically unstable part of the single-phase IM torque-speed curve down to standstill.
- To eliminate direct torque measurements and the load machine system, a slow free acceleration at reduced voltage and a deceleration test may be performed. With the input power, speed and stator currents and voltage measured and parameters already known (from the shortcircuit and single phase no load tests), the torque may be computed after loss subtraction from input at every speed (slip). The torque is also calculated from the motion equation with inertia J determined from free deceleration test. The mechanical power difference in the two measurements should be a good measure of the stray load losses caused by space harmonics. The torque in the torque / speed curve thus obtained is multiplied by the rated to applied voltage ratio squared to obtain the full voltage torque-speed. It is recognized that this approximation underestimates the influence of magnetic saturation on torque at various speeds. This is to say that full voltage load tests from $S = 0$ to $S = 1$ are required for magnetic saturation complete consideration.
- DC flux (current) decay tests at standstill, in the main and auxiliary winding axes, may also be used to determine resistances, leakage inductances and the magnetization curve. Frequency response standstill tests may be applied to single phase IM in a manner very similar to one applied to three phase IMs (Chapter 22).
- Temperature measurement tests are performed as for three phase IMs, in general (Chapter 22).

28.9 REFERENCES

1. C. G. Veinott, Segregation of Losses in Single Phase Induction Motors, Trans AIEE, Volume 54, December 1935, pp. 1302-1306.
2. C. Van der Merwe and F. S. Van der Merwe, Study of Methods to Measure the Parameters of Single Phase Induction Motors, IEEE Trans. vol. EC-10, no. 2, 1995, pp. 248-253.